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Wind Power Applications of Doubly-Fed Reluctance Generators with Parameter-Free Hysteresis Control

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Abstract

The development and practical implementation aspects of a novel scheme for fast power control of the doubly-fed reluctance generator with a low-cost partially-rated converter, a promising brushless candidate for limited speed ranges of wind turbines, are presented in this paper. The proposed concept is derived from the fundamental dynamic analogies between the controllable and measurable properties of the machine: electro-magnetic torque and electrical power, and flux and reactive power. The algorithm is applied in a stationary reference frame without any knowledge of the machine parameters, including rotor angular position or velocity. It is then structurally simpler, easier to realise in real-time and more tolerant of the system operating uncertainties than model-based or proportional-integral control alternatives. Experimental results have demonstrated the excellent controller response for a variety of speed, load and/or power factor states of a custom-built generator prototype.

Keywords: Reactive power control, Sensorless power regulation, Doubly-fed machines, Reluctance generators, Wind turbines.

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1 Nomenclature

- 2 $v_{p,s}$ primary, secondary winding phase voltages [V]
3 $e_{p,s}$ primary, secondary winding back-emf [V]
4 $i_{p,s}$ primary, secondary winding currents [A]
5 $R_{p,s}$ primary, secondary winding resistances [Ω]
6 $L_{p,s}$ primary, secondary 3-phase self-inductances [H]
7 L_m 3-phase mutual inductance [H]
8 σ leakage factor (constant) $= 1 - L_m^2/(L_p L_s)$
9 $\lambda_{p,s}$ primary, secondary winding flux linkages [Wb]
10 λ_m mutual flux [Wb]
11 $\theta_{p,s}$ primary, secondary flux vector angular positions [rad]
12 $\omega_{p,s}$ primary, secondary winding frequencies [rad/s]
13 p, q primary, secondary winding pole-pairs
14 p_r number of rotor poles $= p + q$
15 ω_{rm} rotor angular velocity $= d\theta_{rm}/dt$ [rad/s]
16 θ_r rotor ‘electrical’ angular position $= p_r \theta_{rm}$ [rad]
17 ω_{syn} synchronous speed $= \omega_p/p_r$ [rad/s]
18 P_m total mechanical (shaft) power [W]
19 $P_{p,s}$ primary, secondary mechanical power [W]
20 T_e machine electro-magnetic torque [Nm]
21 P, Q primary real [W] and reactive [VAr] power

1. Introduction

A brushless doubly-fed generator (BDFG) may be an attractive solution to reliability and maintenance issues of carbon brushes and slip-rings with a conventional doubly-excited induction generator (DFIG) while offering competitive performance and the same economic benefits of partial power electronics [1]. For a typical speed ratio of 2:1 in wind energy conversion systems (WECS), the converter derating can be about 75% of the machine itself [2]. In this sense, both the BDFG and DFIG are preferable to heavy and expensive multi-pole wound rotor synchronous generators (SGs) or permanent-magnet generators (PMGs) with fully-rated converters, which are not only costly but more prone to failures undermining the otherwise high reliability of their dedicated wind turbines, gear-less technologies in particular [3]. Another concern for the manufacturers of large PMG units is the risk management of market volatility, availability and payable price premiums of the rare earth magnets (e.g. NdFeB) deployed [4].

With the increasing penetration of distributed generation, the challenging requirements have been imposed by the grid integration codes for the reactive (and real) power support to be provided by WECS to help preserve the transient stability during network disturbances (e.g. voltage sags) [5]. Putting these preventive measures in place has revealed another important BDFG potential, the superior low-voltage-ride-through (LVRT) characteristics owing to the inherently higher impedances and thus lower fault currents relative to the equivalent DFIG [6]. This salient BDFG property could facilitate to a great deal the design of hardware and software protection for the LVRT compliant fractionally-rated converter, decreasing so the supplementary system complexity and cost of DFIG installations [7]. DFIG turbines are known to have LVRT weaknesses and many interesting solu-

47 tions have been recently proposed to find improvements [8]. The latest rigorous
48 review of the extensive research done on this subject has been published in [9].
49 However, tangible practical advances are yet to be made in this direction for the
50 DFIG to become comparable to the PMG, which is amenable to fulfilling the
51 LVRT obligations due to the use of a full-power converter and favorable low volt-
52 age capability curves as demonstrated by the WECS field tests presented in [10].

53 In order to eliminate brushes for reliable and maintenance-free operation, the
54 BDFG has evolved as a self-cascaded inside-out version of the DFIG [11]. This
55 means that the rotor (secondary or control) winding, usually fed from two standard
56 IGBT bridges in bi-directional ('back-to-back') arrangement to allow both super
57 and sub-synchronous speeds in either machine mode, has been moved to the stator
58 and placed together with the grid-connected (primary or power) winding but of
59 different pole number (Fig. 1). The necessary magnetic interaction between the
60 two windings for the torque production is achieved through the rotor with half the
61 total number of the stator poles [12]. Therefore, for the same number of rotor
62 poles and a given line frequency, the DFIG synchronous speed would be twice
63 that of the BDFG making it naturally a medium-speed machine and avoiding the
64 need for a high-speed gear stage of the vulnerable 3-stage gearbox in WECS [1].
65 From this point of view, the BDFG could bring higher efficiency and reliability as
66 well as running costs savings in these applications, and especially off-shore [13].

67 The BDFG reluctance rotor type, the Brushless Doubly-Fed Reluctance Gen-
68 erator (BDFRG in Fig. 1), has several advantages over its 'nested' cage counter-
69 part, the Brushless Doubly-Fed Induction Generator (BDFIG) [12]. Experiments
70 have shown that the BDFRG can be more efficient than the BDFIG of the same
71 stator frame [14]. In addition, the cage-less rotor allows the fewer parameter

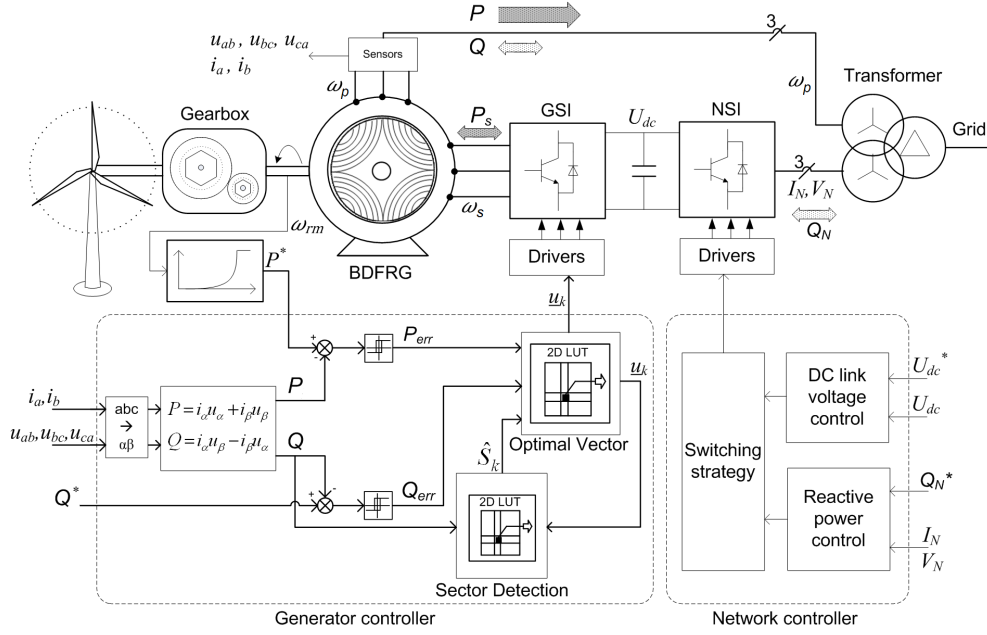


Figure 1: A simplified structural diagram of the BDFRG wind turbine with maximum power point tracking and sensorless hysteresis primary power control of the generator side inverter (GSI).

dependent dynamic modeling, and intrinsically decoupled control of torque and primary reactive power of the BDFRG, unlike the BDFIG [15]. Similar BDFRG attributes are shared by the DFIG [16]. In contrast with the BDFRG or DFIG, the BDFIG has fairly complicated and heavily parameter sensitive model-based vector control [17]. Severe robustness compromises can be affiliated with direct torque controllers for this machine as well [18].

Several 6/2-pole BDFRGs in a kW range have been built, one of which rated at 1.5 kW considered in this paper, and the other to note being a 4 kW counterpart reported in [19]. The more sizeable example recorded in the open literature is a 16 kW, 8/4-pole [12]. One should also mention a 42 kW, 6/2-pole machine studied in [20]. The biggest prototype made so far seems to be a 6/4-pole, 100 kW [21]. An original 2 MW, 6/2-pole design for wind turbines has also been proposed [22].

84 Research paths on control of other machines have been largely pursued in the
85 BDFRG case over the last decade or so. Although intellectually appealing, the
86 non-linear sliding mode control theory developed in [23] has not been applied in
87 practice to be able to judge on its viability. On the other hand, a stator frame ex-
88 ecuted direct torque control (DTC) algorithm has been experimentally verified us-
89 ing a shaft position sensor to generate the speed feedback in [24]. This concept has
90 been adapted for sensorless implementation described in [25]. In the underlying
91 DTC approach, the secondary flux is estimated indirectly from the measured pri-
92 mary voltages and currents at fixed line frequency by virtue of the machine double
93 feeding. The back-emf integration errors (e.g. the integrator saturation) caused by
94 the troublesome resistive effects at low inverter voltages and frequencies, as with
95 the traditional DTC of cage induction motors, have been circumvented but at the
96 expense of conspicuous estimation sensitivity to the BDFRG inductance inaccu-
97 racies and unsatisfactory performance even for an unloaded machine.

98 A better quality response can be attained by the weakly parameter dependent
99 modification of DTC, simulated and experimentally validated in [19]. Further test
100 outcomes using the same scheme have been produced in [26]. The practical stud-
101 ies of the direct power control (DPC) correlative have appeared in [27]. However,
102 these improved ‘direct’ control methods rely on the primary flux estimates and
103 have to face inevitable phase delays and other difficulties commonly associated
104 with filtering noise and transducers DC offset in measurements. Consequently,
105 preliminary no-load results have been mainly shown in all these works.

106 A thorough comparative analysis of vector control (VC) with primary voltage
107 space vector orientation (VOC) and Field (Flux) Oriented Control (FOC) for var-
108 ious loading profiles in motoring mode (BDFRM) has been done experimentally,

109 and by simulations for the BDFRG, in [28]. The latter have been fully labora-
 110 tory demonstrated in [29]. The disturbance rejection abilities of the BDFRG con-
 111 troller(s) have been further explored in [30]. Realistic computer simulation studies
 112 but on a large-scale WECS level have been presented in [31]. The 2 MW BDFRG
 113 design parameters from [22] have been used for this VOC vs FOC performance
 114 comparison. Such VC algorithms offer constant and reduced switching rates en-
 115 tailing generally lower harmonic content, but these preferences over the DT(P)C
 116 may be often undermined by the tuning problems of multiple PI gains with load,
 117 speed and/or machine parameter variations. Besides, an encoder is required for
 118 current control in a rotating frame, even though purely sensorless FOC is feasible
 119 as documented in [32] using the maximum torque per inverter ampere objective.
 120 The encoder-less BDFRG operation under power factor FOC conditions, as an
 121 extension of this experimental VC research, has been elaborated in [33].

122 The hysteresis real and reactive power control (HPQC) strategy put forward
 123 in this paper can overcome most of the previously addressed shortcomings of
 124 the concurrent torque and/or power control methodologies for the BDFRG. As
 125 the name implies, the underlying idea is to govern the accessible terminal quan-
 126 tities rather than electro-magnetic torque and/or flux, which are internal to the
 127 machine and susceptible to estimation errors. The obvious advantages over the
 128 DTC gained in this way are the algorithm simplicity and higher accuracy, the po-
 129 tential downside being the drive train speed oscillations that may occur in the lack
 130 of immediate torque regulation with large wind turbines [2]. Furthermore, unlike
 131 the DT(P)C methods discussed earlier, the total parameter-freedom (i.e. an exclu-
 132 sive reliance on measurements, and not estimates) makes the HPQC more robust,
 133 simpler and faster to execute. It is also easier to implement than the VC by the

134 absence of PI tuning and intrinsic immunity to parameter uncertainties.

135 2. Background theory

136 The space-vector model of the *primary* and *secondary* windings in a stator
 137 $\alpha - \beta$ frame (Fig. 2) with standard notation and adopting *motoring* (BDFRM)
 138 convention can be represented as [34]:

$$\left. \begin{aligned} \underline{e}_p &= \frac{d\lambda_p}{dt} = \underline{u}_p - R_p \underline{i}_p = \frac{d\lambda_p}{dt} e^{j\theta_p} + j\omega_p \lambda_p \\ \underline{e}_s &= \frac{d\lambda_s}{dt} = \underline{u}_s - R_s \underline{i}_s = \frac{d\lambda_s}{dt} e^{j\theta_s} + j\omega_s \lambda_s \\ \lambda_p &= \lambda_p e^{j\theta_p} = L_p \underline{i}_p + L_m \underline{i}_s^* e^{j\theta_r} = L_p \underline{i}_p + L_m \underline{i}_{sm}^* \\ \lambda_s &= \lambda_s e^{j\theta_s} = L_s \underline{i}_s + L_m \underline{i}_p^* e^{j\theta_r} = L_s \underline{i}_s + L_m \underline{i}_{pm}^* \end{aligned} \right\} \quad (1)$$

139 where \underline{i}_{pm} and \underline{i}_{sm} are the magnetically coupled (magnetizing) current vectors
 140 which come from the actual primary (\underline{i}_p) and secondary (\underline{i}_s) current counterparts
 141 rotating at different velocities as shown in Fig. 2. This peculiar frequency mod-
 142 ulation through the rotor is hidden in the $e^{j\theta_r}$ term in (1). Note from Fig. 2 that
 143 $\underline{i}_{pm} = \underline{i}_p = \underline{i}_p e^{j\varepsilon}$ and $\underline{i}_{sm} = \underline{i}_s = \underline{i}_s e^{j\gamma}$ in the corresponding frames [35].

The electro-mechanical energy conversion in the machine takes places under
 the following angular velocity and pole conditions with the mechanical power
 relationships showing contributions of each winding [35]:

$$\omega_{rm} = \frac{\omega_p + \omega_s}{p + q} = \left(1 + \frac{\omega_s}{\omega_p}\right) \cdot \frac{\omega_p}{p_r} = \left(1 + \frac{\omega_s}{\omega_p}\right) \cdot \omega_{syn} \quad (2)$$

$$P_m = T_e \cdot \omega_{rm} = \frac{T_e \cdot \omega_p}{p_r} + \frac{T_e \cdot \omega_s}{p_r} = P_p + P_s \quad (3)$$

144 where $\omega_s > 0$ for super-synchronous operation ($\omega_{rm} > \omega_{syn}$), $\omega_s < 0$ at sub-

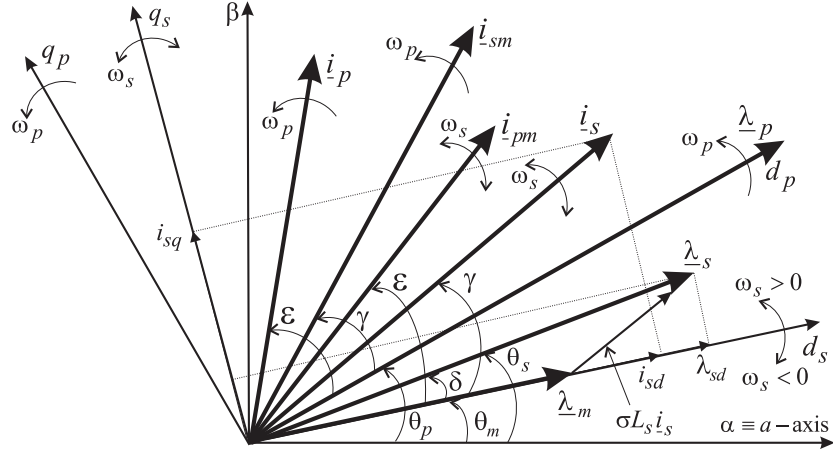


Figure 2: Characteristic space vectors and flux-oriented reference frames.

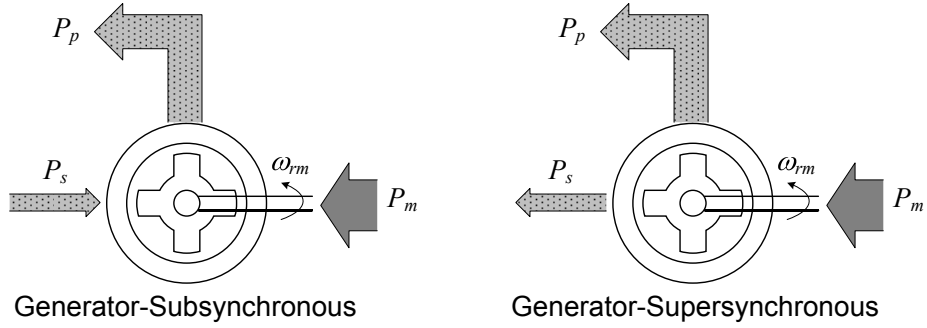


Figure 3: Reference (positive) power flow in the BDFRG for the two speed modes.

145 synchronous speeds¹($\omega_{rm} < \omega_{syn}$), and $\omega_s = 0$ (i.e. DC secondary winding) in
 146 synchronous speed mode ($\omega_{rm} = \omega_{syn}$) as with a classical $2p_r$ -pole wound field
 147 turbo-machine. A power flow diagram conforming to (3) appears in Fig. 3.

¹The ‘negative’ frequency in this speed region simply means the opposite phase sequence of the secondary to the primary winding i.e. ‘clockwise’ rotation of the respective vectors in the $d_s - q_s$ frame as indicated in Fig. 2.

148 3. Principles and architecture of hysteresis control

149 Using (3), the primary electrical power can be approximated as [24]:

$$P \approx P_p = \underbrace{\frac{3p_r}{2\sigma L_s} |\underline{\lambda}_m \times \underline{\lambda}_s|}_{T_e} \cdot \frac{\omega_p}{p_r} = \frac{3\omega_p}{2\sigma L_s} \underbrace{\frac{L_m}{L_p} \lambda_p \lambda_s}_{\lambda_m} \sin \delta \quad (4)$$

150 where λ_p , and thus λ_m , amplitudes are almost constant because of the primary
 151 winding grid connection. The real power can therefore be controlled in a station-
 152 ary frame through $\underline{\lambda}_s$ angle, θ_s , i.e. $\delta = \theta_s - \theta_m$ (Fig. 2) in a DTC fashion as
 153 slowly ω_s varying θ_m can hardly change during a short control interval [24].

154 The Q control notion has been deduced from the fact that the two windings
 155 jointly participate in the production of the nearly fixed air-gap flux in the BD-
 156 FRG. So, if one winding subsidizes more, the other should commit less in the
 157 flux build-up. Since the electro-magnetic phenomena are strongly linked with the
 158 magnetizing currents, and hence the respective flux values, one can map the pri-
 159 mary Q variations with those on the controllable secondary side in the sense that
 160 Q increase/decrease could be obtained by reducing/increasing the λ_s . These intu-
 161 itive hypotheses are easiest to prove mathematically using the Q expression that
 162 can be derived from the steady-state FOC form of (1) [30]:

$$Q = \frac{3}{2} \omega_p \left(\frac{\lambda_p^2}{L_p} - \lambda_m i_{sd} \right) = \frac{3e_p}{2L_p} (\lambda_p - L_m i_{sd}) \quad (5)$$

163 where $e_p = \omega_p \lambda_p \approx u_p$. Given that $\lambda_p \approx \text{const}$, the higher Q from the grid, the
 164 lower i_{sd} , and thus i_s , and vice-versa. Since $\sigma L_s i_s$ has qualitatively the same trend
 165 as i_s does, so will λ_s in keeping with the phasor diagram in Fig. 2.

166 3.1. Inverter voltages and power implications

167 The usual binary representations of the inverter legs switching status for the
 168 six active voltage vectors (\underline{u}_k), where k denotes the attributed sector number (e.g.
 169 $\underline{u}_1 \equiv 100$, $\underline{u}_2 \equiv 110$ etc.), are illustrated in Fig. 4. It has been shown in [24] that
 170 the other two zero-vectors (e.g. ‘111’ and ‘000’) have contradictory influences
 171 on T_e and λ_s behavior above and below the synchronous speed (ω_{syn}). The same
 172 holds true for their dual quantities, P and Q , which are control variables here.
 173 So, precise speed sensing or estimation is imperative in this case, and especially
 174 near or at ω_{syn} where the BDFRG is normally operated anyway. Needless to
 175 say that this would complicate the HPQC design and implementation. Another
 176 serious challenge in this speed region, which is equivalent to a low frequency
 177 operation of cage induction machines, are the detrimental R_s effects leading to
 178 unwanted λ_s weakening and control degradation if the zero vectors are repeatedly
 179 applied at low u_s and ω_s values. For these reasons, they have not been employed
 180 with an incentive to facilitate the HPQC and retain its speed independence in
 181 either operating mode. Somewhat higher switching rates are clearly unavoidable
 182 to accommodate these conveniences, but this compromise is more than offset by
 183 the acquired performance boost.

184 The $\underline{\lambda}_s$, and thus P , dynamics depend on the flux instant position. For exam-
 185 ple, if $\underline{\lambda}_s$ is in sector 1 as shown in Fig. 4, applying either \underline{u}_2 or \underline{u}_3 to the BDFRM
 186 would shift $\underline{\lambda}_s$ anti-clockwise increasing both δ and $P > 0$ according to (4). With
 187 $\underline{\lambda}_m$ leading $\underline{\lambda}_s$ for the BDFRG, the same voltages would reduce δ but likewise
 188 increase $P < 0$ (i.e. less positive power produced by the primary) as in the BD-
 189 FRM case when the power flow is reversed (negative). On the other hand, \underline{u}_6 or \underline{u}_5
 190 have totally opposite effects on P to \underline{u}_2 or \underline{u}_3 regardless of the machine operating

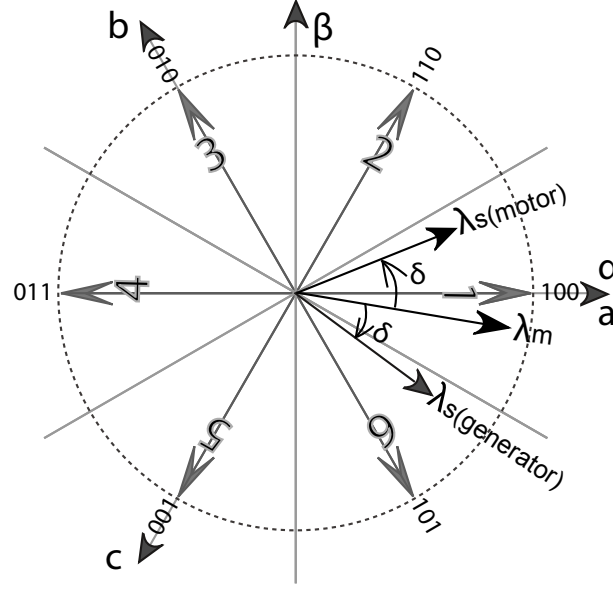


Figure 4: Active voltage vectors of two-level inverter fed secondary and respective 60° sectors in a stator plane.

regime. The voltage vectors to be applied to get the desired P increments for each individual $\underline{\lambda}_s$ sector in Fig. 4 are given in Table 1.

As already discussed, the Q control can be accomplished by varying λ_s . It can be seen from Fig. 4 that either \underline{u}_1 , \underline{u}_2 or \underline{u}_6 would result in an increase of λ_{sd} (Fig. 2), and hence λ_s i.e. a decrease of Q . The impacts of \underline{u}_3 , \underline{u}_4 or \underline{u}_5 would be such to ask for more Q from the grid, and are again the operating mode invariant as with the P control scenarios. The inverter voltages requested to meet the specific dQ demands for a given $\underline{\lambda}_s$ sectorial position can be found in Table 1 too.

3.2. Control procedure

A HPQC schematic is shown in Fig. 1. The P^* and Q^* set-points refer to the optimum performance indicators of interest to a particular application e.g. P^* for the maximum power point tracking (MPPT) and Q^* to get near unity power

Table 1: Secondary voltage effects on primary power differentials

<i>Sector \ Change</i>	$dP > 0$	$dP < 0$	$dQ > 0$	$dQ < 0$
1	$\underline{u}_2, \underline{u}_3$	$\underline{u}_6, \underline{u}_5$	$\underline{u}_5, \underline{u}_3, \underline{u}_4$	$\underline{u}_6, \underline{u}_1, \underline{u}_2$
2	$\underline{u}_3, \underline{u}_4$	$\underline{u}_1, \underline{u}_6$	$\underline{u}_6, \underline{u}_4, \underline{u}_5$	$\underline{u}_1, \underline{u}_2, \underline{u}_3$
3	$\underline{u}_4, \underline{u}_5$	$\underline{u}_2, \underline{u}_1$	$\underline{u}_1, \underline{u}_5, \underline{u}_6$	$\underline{u}_2, \underline{u}_3, \underline{u}_4$
4	$\underline{u}_5, \underline{u}_6$	$\underline{u}_3, \underline{u}_2$	$\underline{u}_2, \underline{u}_6, \underline{u}_1$	$\underline{u}_3, \underline{u}_4, \underline{u}_5$
5	$\underline{u}_6, \underline{u}_1$	$\underline{u}_4, \underline{u}_3$	$\underline{u}_3, \underline{u}_1, \underline{u}_2$	$\underline{u}_4, \underline{u}_5, \underline{u}_6$
6	$\underline{u}_1, \underline{u}_2$	$\underline{u}_5, \underline{u}_4$	$\underline{u}_4, \underline{u}_2, \underline{u}_3$	$\underline{u}_5, \underline{u}_6, \underline{u}_1$

factor (e.g. typically between 0.95 lagging and leading) for WECS [4]. The 3-phase power inputs to the hysteresis comparators are generated from the stationary $\alpha\beta$ components (Fig. 2) of the line current and voltage measurements for the Y-connected primary winding with an isolated neutral point and ‘abc’ phase sequence (Fig. 4) as follows:

$$\left. \begin{aligned} P &= \underbrace{i_a}_{i_\alpha} \cdot \underbrace{\frac{u_{ab} + u_{ac}}{2}}_{u_\alpha} + \underbrace{\frac{i_a + 2i_b}{\sqrt{3}}}_{i_\beta} \cdot \underbrace{\frac{\sqrt{3}u_{bc}}{2}}_{u_\beta} \\ Q &= \underbrace{i_a}_{i_\alpha} \cdot \underbrace{\frac{\sqrt{3}u_{bc}}{2}}_{u_\beta} - \underbrace{\frac{i_a + 2i_b}{\sqrt{3}}}_{i_\beta} \cdot \underbrace{\frac{u_{ab} + u_{ac}}{2}}_{u_\alpha} \end{aligned} \right\} \quad (6)$$

The integer error outputs from the comparators (Fig. 1), P_{err} and Q_{err} , and the secondary flux sector number (k) allow to retrieve the relevant inverter switching information from the look-up tables for a suitable secondary voltage vector to simultaneously satisfy the dP and dQ control specifications in line with Table 1 where the highlighted \underline{u}_k and \underline{u}_{k+3} vectors are not applicable. Implementing the switching logic as per the resulting Table 2 in the controller should make sure that the instantaneous P and Q values are kept within the user-defined hysteresis bands around the reference trajectories i.e. $[P^* - \delta P, P^* + \delta P]$ and $[Q^* - \delta Q, Q^* + \delta Q]$.

Table 2: Inverter switching vectors

Power Deviations		Sector(k)					
$P^* - P$	$Q^* - Q$	1	2	3	4	5	6
$\leq -\delta P$	$> \delta Q$	\underline{u}_5	\underline{u}_6	\underline{u}_1	\underline{u}_2	\underline{u}_3	\underline{u}_4
$\leq -\delta P$	$\leq -\delta Q$	\underline{u}_6	\underline{u}_1	\underline{u}_2	\underline{u}_3	\underline{u}_4	\underline{u}_5
$> \delta P$	$> \delta Q$	\underline{u}_3	\underline{u}_4	\underline{u}_5	\underline{u}_6	\underline{u}_1	\underline{u}_2
$> \delta P$	$\leq -\delta Q$	\underline{u}_2	\underline{u}_3	\underline{u}_4	\underline{u}_5	\underline{u}_6	\underline{u}_1

Table 3: Anticipated ΔQ sign (\pm) | Flux sector increments (± 1)

k	\underline{u}_1	\underline{u}_2	\underline{u}_3	\underline{u}_4	\underline{u}_5	\underline{u}_6
1		- - 1	+ + 1		+ - 1	- + 1
2	- + 1		- - 1	+ + 1		+ - 1
3	+ - 1	- + 1		- - 1	+ + 1	
4		+ - 1	- + 1		- - 1	+ + 1
5	+ + 1		+ - 1	- + 1		- - 1
6	- - 1	+ + 1		+ - 1	- + 1	

216 3.3. Secondary flux sector ascertainment

217 One of the principal strengths of the proposed HPQC over DTC is a unique
 218 $\underline{\lambda}_s$ sector identification technique, which is not founded on the $\underline{\lambda}_s$ estimation or
 219 its absolute position knowledge, but on monitoring the measurable incremental
 220 changes of Q (ΔQ) instead. On these grounds, it is essentially indirect in principle
 221 and allows entirely parameter independent sensorless power control.

222 Commencing with the case study considered in Fig. 4 as an initial $\underline{\lambda}_s$ sector
 223 condition and looking at the possible voltage-sector combinations from Tables 2
 224 and 3, if \underline{u}_2 or \underline{u}_6 are applied then $\Delta Q < 0$ (i.e. ‘-’ in Table 3), else \underline{u}_3 or \underline{u}_5 are
 225 the secondary terminal voltages and $\Delta Q > 0$ (i.e. ‘+’ in Table 3). So, as long
 226 as the predictions in the ΔQ sign (Table 3) are coincident with the calculations
 227 obtained from measurements using (6), no control action for the sector transition

228 should be taken. Otherwise, any disagreement in the results may suggest that
 229 an unknown machine speed mode reliant sector change has occurred, and that
 230 the sector counter is to be updated by ± 1 as $\underline{\lambda}_s$ can't instantly 'jump' through
 231 the sectors. At super-synchronous speeds, the $\underline{\lambda}_s$ rotating counter-clockwise goes
 232 to sector 2 where \underline{u}_3 and \underline{u}_6 have completely different effects on ΔQ than in
 233 sector 1, causing a sudden alteration of the ΔQ sign and hence the sector number.
 234 Similarly, for sub-synchronous speed operation and clockwise rotation of $\underline{\lambda}_s$, \underline{u}_2
 235 and \underline{u}_5 are the two pointing vectors to a sector change from 1 to 6.

236 **4. Experimental verification**

237 The HPQC scheme was implemented on a dSPACE[®] control prototyping plat-
 238 form of a custom-made test rig (Fig. 5) for a 6/2-pole BDFRG with both windings
 239 rated at 415 V, 2.5 A, 50 Hz. The two-level voltage source inverter is a Semikron[®]
 240 smart power IGBT module (Skiip[®]). A commercial four-quadrant Parker[®] DC
 241 drive emulated the chosen prime mover (e.g. wind turbine) characteristics of the
 242 BDFRG as explained in [36]. The remaining machine data and details of the appa-
 243 ratus used for testing can be found in [30]. The system sampling rate was 10 kHz,
 244 and the variable switching frequency was around 5 kHz. The hysteresis bands
 245 were set to $\delta P = 50$ W and $\delta Q = 100$ VAR.

246 The BDFRG is self-started as a wound rotor induction machine to the steady
 247 no-load speed (Fig. 6). The inverter was then enabled, and the HPQC viability
 248 proven by laboratory tests for three ordinary speed set-points in a narrow range
 249 down to synchronous speed for step-changes in P^* and/or Q^* settings. An incre-
 250 mental encoder was used for instrumentation purposes and to provide feedback to
 251 the DC drive to maintain the desired shaft speed externally as the prime objective

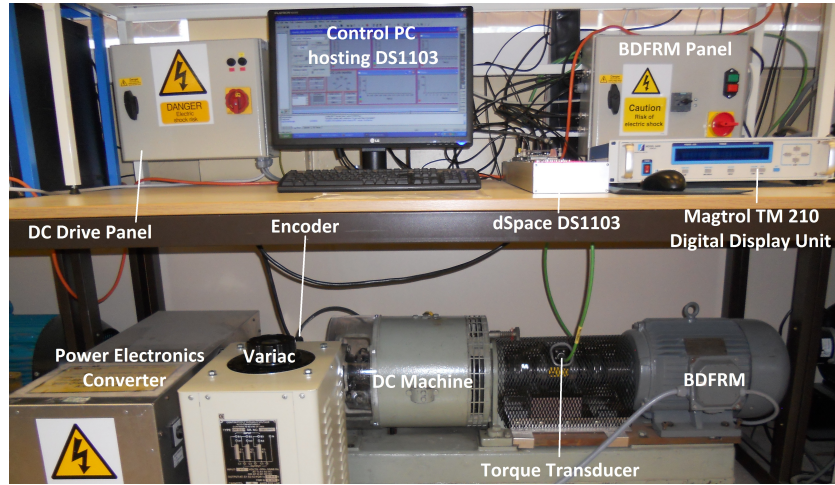


Figure 5: A photo of the BDFRG test facility used for experimental studies.

was to evaluate the algorithm in power mode. In a real WECS with HPQC, this sensor would only serve to generate the necessary P^* for MPPT [1] as depicted in Fig. 1. Sensorless MPPT options are also possible [37].

4.1. Open-loop speed control

The P step response in Fig. 7 demonstrates the high HPQC performance with a smooth and swift changeover from BDFRM to BDFRG operation. For instance, such mode reversals, whilst not so rapid, are encountered with reversible pump-turbine devices for load balancing in pumped-storage hydro-power plants [38, 39]. The corresponding Q trace shows little or no apparent signs of cross-coupling pertaining to this transition, such as level shifting or other steady-state disturbances. A short speed dip comes from the sudden load increase perceived by the DC machine when the BDFRG starts generating P . For a given Q^* , the primary current (i_a) is virtually unaffected by the P transient with its largely magnetizing nature,



Figure 6: Oscillograms of the recorded steady-state currents in two phases of the shorted secondary winding for the unloaded BDFRG at ≈ 730 rev/min.

and hence the fairly uniform magnitudes throughout at line frequency². However, the peak secondary currents (i_{s_a}), as predominantly torque producing, get higher to accommodate the rise of T_e and P_m required to cover the BDFRG losses in delivering the same P as consumed for the BDFRM operation. Moreover, from (2), the secondary frequency (f_s) should be -6.67 Hz at 650 rev/min, which can be found indeed true by counting ≈ 17 150 ms cycles over 2.5 s on the measured i_{s_a} waveform. The last graph in Fig. 7 shows the descending sector changes of the clockwise rotating λ_s as ' $f_s < 0$ ' (Figs. 2 and 4).

The results in Fig. 8 are complementary to those in Fig. 7. The HPQC properties for a stepping-down Q^* and the 'idling' machine playing an inductive role are

²There are 25 sine waves, each of 20 ms in period (i.e. $f_p = 50$ Hz), over a 0.5 s time interval on the respective zoomed-in sub-plot of Fig. 7.

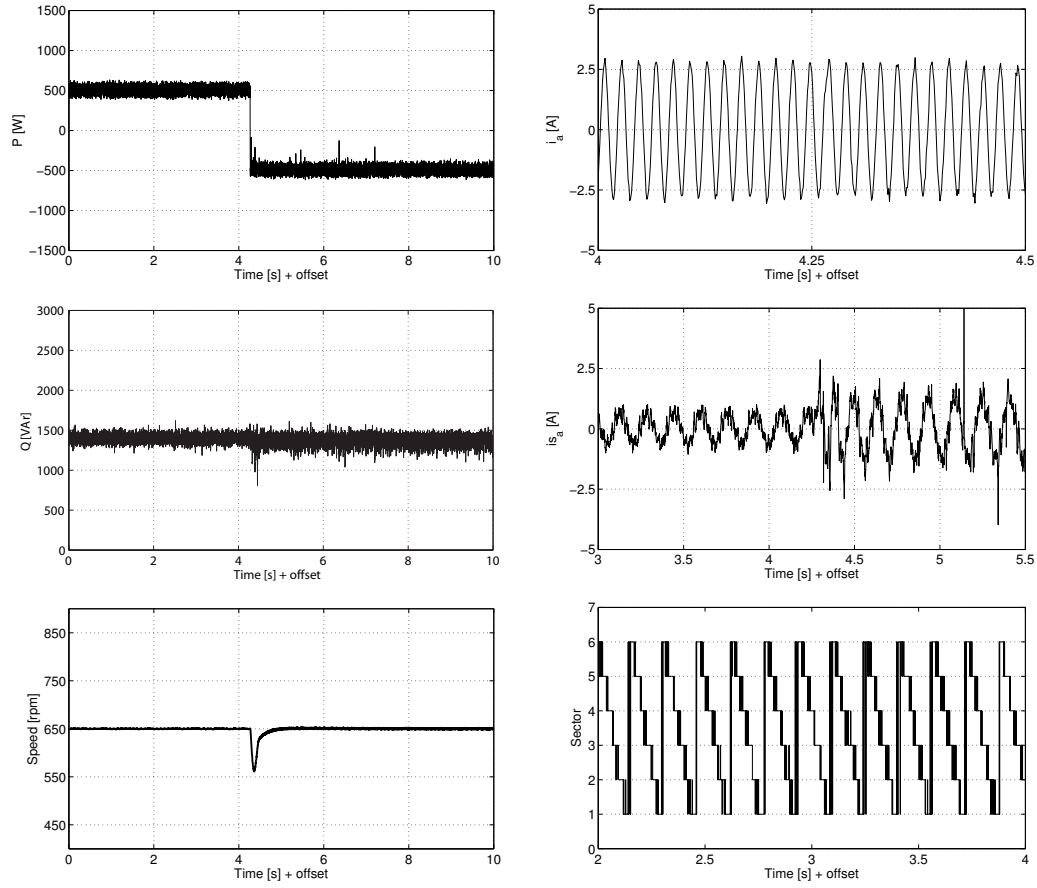


Figure 7: BDFRG(M) performance in sub-synchronous mode at 650 rev/min for $P^* = \pm 500$ W and $Q^* = 1.35$ kVar.

275 examined now. Albeit not practical, this is an extremely insightful and challeng-
 276 ing scenario from a control perspective as the current in either winding is then
 277 mostly reactive allowing the effects of varying Q^* to be investigated separately
 278 from P . The power plots in Fig. 8 are another evidence of robust and decoupled
 279 control, although fast transients may be superfluous for the BDFRG target appli-
 280 cations. Unlike Fig. 7, the shaft speed is barely influenced by the Q^* change as
 281 expected for an unloaded machine, whereas the i_a amplitudes exhibit a foreseen
 282 decline with the Q reduction. The magnetizing i_{sa} will thus increase in magnitude
 283 in much the same manner as it does in Fig. 7.

284 The measurements in Fig. 9 reinforce the controller's ability to successfully
 285 track the stipulated Q^* trajectory, and its capacity to instantly react to an even
 286 doubled step-change of Q^* than in Fig. 8. More importantly, the mid P value
 287 doesn't seem to be impaired in any way by such a big Q perturbation. The i_a
 288 has notably decreased, and the i_{sa} , taking over the machine magnetization from
 289 the primary winding increased, in magnitude nearly in the same proportion as the
 290 Q level has diminished with the power factor improvement indicating the mainly
 291 flux producing share of both the currents.

292 The majority of the HPQC observations and/or explanations of the physical
 293 phenomena behind the performance measures in Fig. 7 can be extended to those
 294 in Fig. 10. An exception is that the $\underline{\lambda}_s$ sector numbers are now in ascending order
 295 with the counter-clockwise rotation of the secondary vectors, which is contrary to
 296 the sub-synchronous case in Fig. 7. By analogy to the latter, the presence of ≈ 30
 297 cycles on the i_{sa} waveform in Fig. 10 during 4.5 s implies the same f_s of 6.67 Hz
 298 at 850 rev/min and 650 rev/min but with the opposite phase sequence (i.e. sign).

299 Much the same HPQC features can be discovered from Fig. 11. Either Q or P

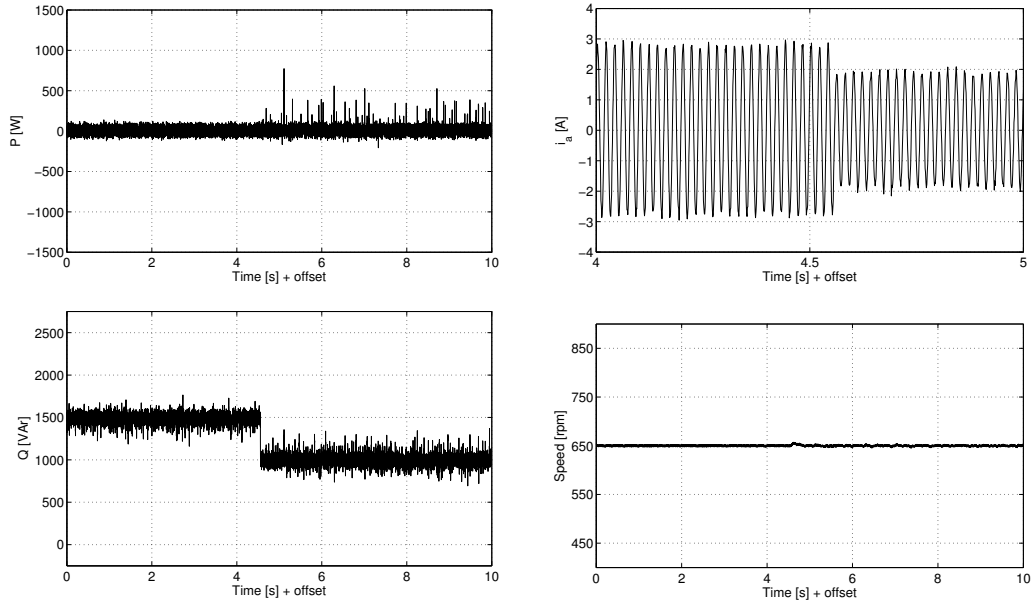


Figure 8: BDFRG sub-synchronous response to Q^* step change from 1.5 kVar to 1 kVar at 650 rev/min in 'stand-by' power mode (i.e. $P^* = 0$).

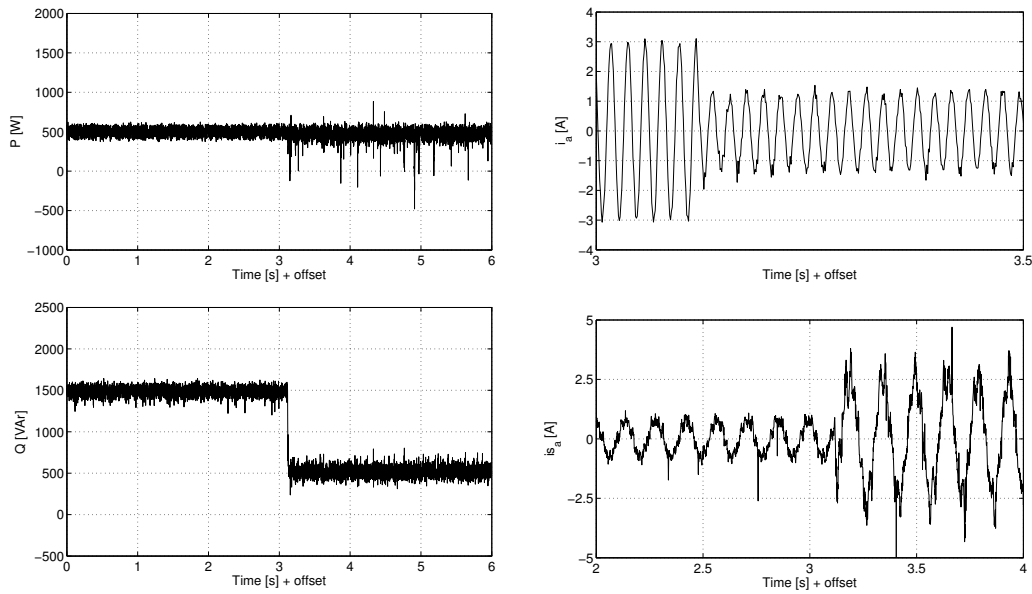


Figure 9: BDFRM response to a sudden Q^* drop from 1.5 kVar to 500 VAr at 850 rev/min and $P^* = 500$ W in super-synchronous mode.

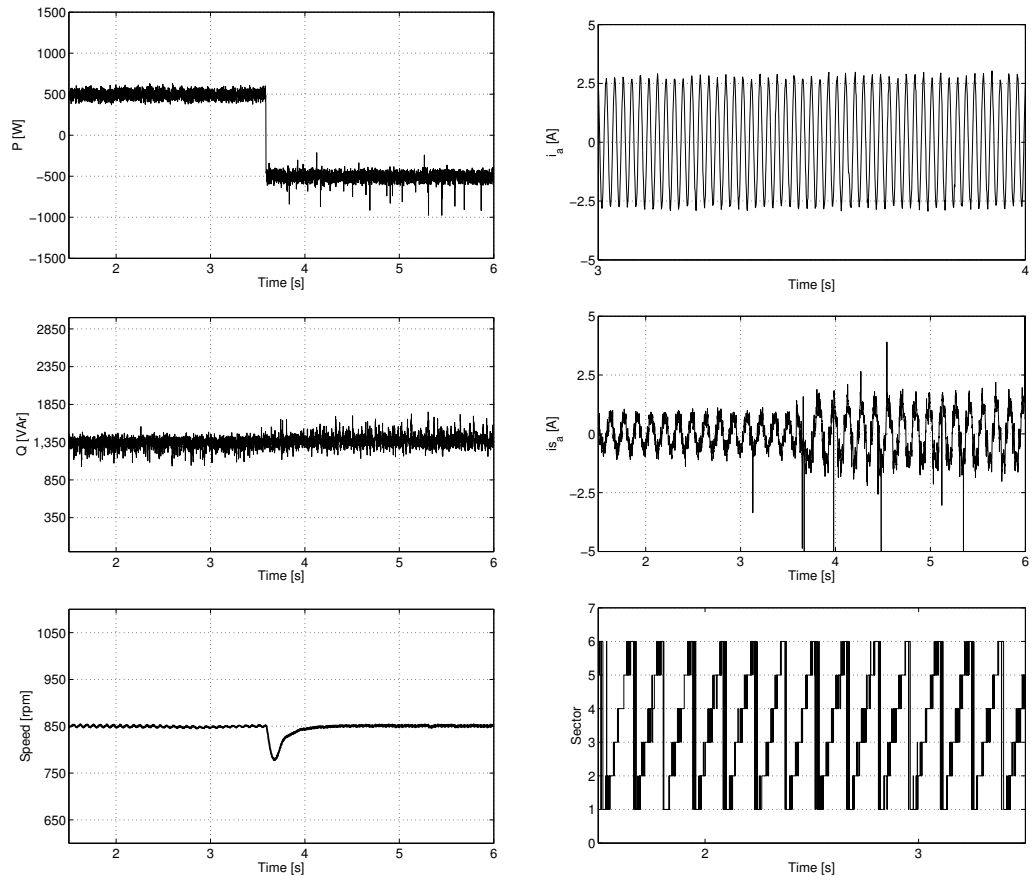


Figure 10: Super-synchronous operation of the BDFRG(M) at 850 rev/min, $P^* = \pm 500$ W and $Q^* = 1.35$ kVar.

300 responses appear to be insensitive (in average terms) to the step-down change of
 301 P^* not experiencing any visible form of disruption. The speed glitch in Fig. 11 is
 302 less pronounced than in Fig. 7 because of the smaller P^* deviation to handle by
 303 the PI speed regulator of the DC drive.

304 4.2. Machine design vs performance trade-offs

305 Noise susceptibility and higher current ripples are common with hysteresis
 306 control by its ‘bang-bang’ complexion. This issue is more aggravated for the
 307 considered HPQC with the non-optimal design of the proof-of-concept BDFRG.
 308 The latest finite-element-analyses [40] have established the drawbacks of 6/2 pole
 309 winding arrangements with an axially-laminated rotor in terms of the presence of
 310 low-order harmonics and modest power density, identifying the 8/4 pole wound
 311 stators and modern radially-laminated ducted reluctance rotors as the way for-
 312 ward. The former limitation can clearly be attributed to our prototype judging by
 313 the visibly ripple-corrupted i_s waveforms even without switching power electron-
 314 ics in Fig. 6 and manifesting also in Figs. 7, 9 and 10 for the controlled machine.
 315 Similar distortions, though incomparably less pronounced with the weak mag-
 316 netic coupling between the windings, can be seen in the much cleaner primary i_a
 317 currents but only at higher i_s levels in Fig. 9. Furthermore, the unusually large
 318 $R_s \approx 13 \Omega$ contravenes the main HPQC assumption of negligible voltage drops
 319 causing modeling and control inaccuracies as \underline{e}_s , and not \underline{u}_s , dictates $d\lambda_s/dt$ given
 320 (1). This is predominantly the case with increasing i_s amplitudes as can be seen in
 321 the spiky P , and foremost Q , responses (Figs. 7, 9 and 10) or at lower Q^* require-
 322 ments (Figs. 8, 9 and 11). Finally, the secondary winding is not appropriately
 323 rated to accomplish close to unity primary power factor without tripping of the
 324 over-current protection.

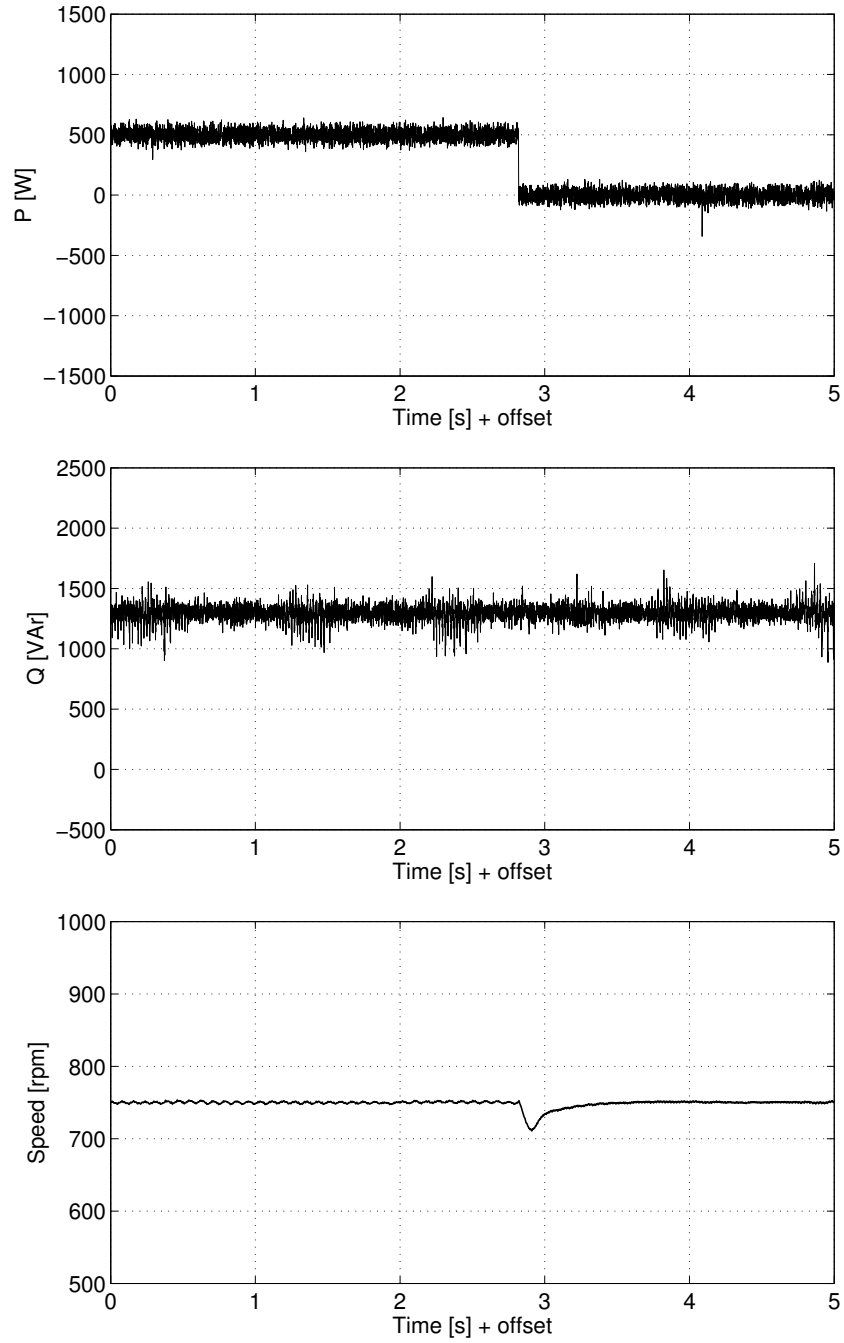


Figure 11: BDFRM synchronous operation at 750 rev/min for P^* varying from 500 W to 0 W at 1.3 kVAr.

325 5. Conclusions

326 A robust, machine parameter-free HPQC algorithm for the BDFRG has been
327 suggested and successfully experimentally validated by the results presented. Orig-
328 inating from the basic electro-magnetic relationships for doubly-excited machines
329 renders it versatile and potentially suitable to any member of this family. These
330 virtues, coupled with the computational effectiveness and ease of implementation,
331 offer superior performance to the existing DT(P)C methods and could strengthen
332 the HPQC standing as a viable competitor of model-based or PI control strategies.

333 A good overall response and disturbance rejection abilities of both the P and
334 Q sub-controllers have been verified on the early small-scale prototype despite the
335 challenging test conditions imposed by its high winding resistances and crude de-
336 sign. The adverse resistive effects and control deterioration at low secondary volt-
337 ages and frequencies over a narrow speed range have been mitigated by omitting
338 the two zero-vectors in the inverter switching strategy being applied. The accom-
339 panying speed mode reliance and emanating complexities of the HPQC scheme,
340 that would have been otherwise introduced, had been avoided as an added bonus.
341 A significant performance enhancement would be envisaged with larger, more
342 representative machines having much smaller resistances.

343 The above merits, along with the rotor position and velocity independence,
344 form a basis for facilitated sensorless HPQC of WECS, or the use of a low to
345 medium resolution sensor, as very accurate estimates or high-bandwidth mea-
346 surements of the shaft speed are not imperative for MPPT in these applications.
347 Finally, given the conceptual similarities with the DTC, the HPQC may draw at-
348 tention of industrial companies like ABB with its existing production line of DTC
349 power converters (e.g. the ACS800 series) for DFIG and other MW wind turbines.

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